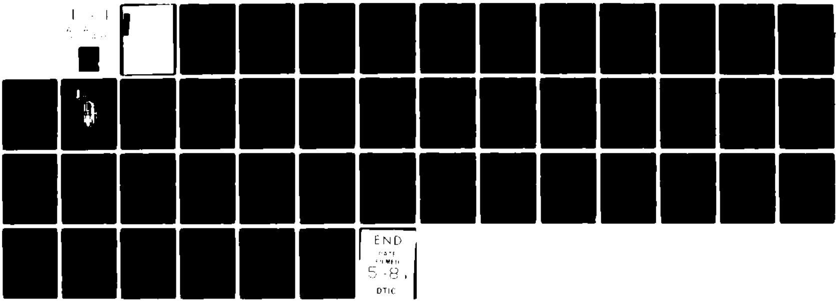
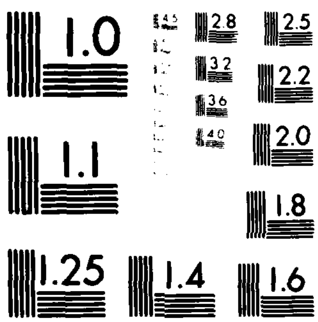
AD-A097 485 NAVAL RESEARCH LAB WASHINGTON DC F/G 18/1
A LINUS FUSION REACTOR DESIGN BASED ON AXISYMMETRIC IMPLOSION O--ETC(U)
APR 81 P J TURCHI, A L COOPER, D J JENKINS
UNCLASSIFIED NRL-MR-4388 NL





MICROCOPY RESOLUTION TEST CHART

NATIONAL BUREAU OF STANDARDS-1963-A

(9) Memorandum rept.,

SECURITY CLASSIFICATION OF THIS PAGE (When Data Entered)

REPORT DOCUMENTATION PAGE		READ INSTRUCTIONS BEFORE COMPLETING FORM
1. REPORT NUMBER (14) NRL-MR- NRL Memorandum Report 4388	2. GOVT ACCESSION NO. AD-A097	3. RECIPIENT'S CATALOG NUMBER 485
4. TITLE (and Subtitle) (6) A LINUS FUSION REACTOR DESIGN BASED ON AXISYMMETRIC IMPLOSION OF TANGENTIALLY-INJECTED LIQUID METAL.		5. TYPE OF REPORT & PERIOD COVERED Interim report on a contuniing NRL problem.
		6. PERFORMING ORG. REPORT NUMBER
7. AUTHOR(s) (10) P. J. Turchi, A. L. Cooper, D. J. Jenkins and E. P. Scannell**		8. CONTRACT OR GRANT NUMBER(s) (11) 2 Apr 81
9. PERFORMING ORGANIZATION NAME AND ADDRESS Naval Research Laboratory Washington, DC 20375 (12) 45		10. PROGRAM ELEMENT, PROJECT, TASK AREA & WORK UNIT NUMBERS 61153N-11; RR00-09-41; 47-0877-0-1
11. CONTROLLING OFFICE NAME AND ADDRESS (16) RR0009		12. REPORT DATE April 2, 1981
		13. NUMBER OF PAGES 46
14. MONITORING AGENCY NAME & ADDRESS (if different from Controlling Office) (17) RR000941		15. SECURITY CLASS. (of this report) UNCLASSIFIED
		15a. DECLASSIFICATION/DOWNGRADING SCHEDULE

16. DISTRIBUTION STATEMENT (of this Report)

Approved for public release; distribution unlimited.

17. DISTRIBUTION STATEMENT (of the abstract entered in Block 20, if different from Report)

18. SUPPLEMENTARY NOTES

*Present address: R & D Associates 1401 Wilson Blvd., Arlington, VA 22209
**Present address: JAYCOR, Inc., Alexandria, VA 22304

19. KEY WORDS (Continue on reverse side if necessary and identify by block number)

Axisymmetric
Liquid metal liner implosion
Toroid plasmas
LINUS
Fusion reactor

20. ABSTRACT (Continue on reverse side if necessary and identify by block number)

Imploding liner techniques have been studied at the Naval Research Laboratory both experimentally and theoretically. The results of such studies are applied in this report to the design of a fusion power reactor based on the axisymmetric implosion of a rotating liquid metal cylinder created by tangential injection. Scaling laws are developed from one-dimensional compressible fluid code calculations and are used to define performance trends. High mass density liners (Pb-Li) and modest compression ratios (10:1 in radius) appear to be favored. A self-consistent point design is developed for a 500 MW(e) power reactor operating with a circulating power fraction of less than 15 per cent. Directions for further work are discussed.

DD FORM 1 JAN 73 1473 EDITION OF 1 NOV 65 IS OBSOLETE
S/N 0102-LF-014-6601

SECURITY CLASSIFICATION OF THIS PAGE (When Data Entered)

251950

CONTENTS

A LINUS Fusion Reactor Design Based on Axisymmetric Implosion of Tangentially-Injected Liquid Metal

I. INTRODUCTION

A primary goal of the liner implosion research program[1] at NRL has been the development of safe, repetitive implosions of metal cylinders or liners for use in compressing plasma adiabatically to fusion conditions. It has, of course, been recognized for some time[2,3] that by magnetic flux compression the implosion of electrically-conducting liners can generate multi-megagauss magnetic fields capable of supporting plasmas at temperatures of 10 keV and densities in excess of 10^{18} cm^{-3}, for which the so-called Lawson time for nuclear energy gain would be less than 80 μsec. The basic difficulties in using liner implosions for fusion power reactors have been two fold: the creation and maintenance of an initial plasma-magnetic field payload suitable for compression by liner implosion; and the development of liner implosion technology that is safe, efficient and economical when used on the repetitive basis required for power reactors. The former difficulty may be resolved if efforts are successful in developing compact toroid plasmas.[4] The latter requirement may be solved by application of concepts and technology developed at NRL for liquid metal liner implosions. This report details a fusion reactor design based on actual theoretical and experimental results from the NRL liner implosion research program.

Manuscript submitted December 31, 1980.

II. BRIEF SUMMARY OF NRL LINER IMPLOSION PROGRAM

At NRL, various liner implosion techniques have been demonstrated and studied over the last several years to provide an experimental and theoretical base for understanding their fundamental and practical advantages and/or limitations. Electromagnetic implosions of solid aluminum and copper liners were demonstrated using solenoidal magnetic field coils driven by a capacitor bank and inductive store.[5] Radial compression ratios of 28:1 and peak compressed magnetic fields of 1.4 Mgauss were obtained with cm-diameter clear bores. Rotating liners of liquid sodium-potassium alloy were also imploded electromagnetically,[6] providing the first experimental verification of rotational stabilization[7] of Rayleigh-Taylor modes on the inner, decelerating interface between liquid metal and vacuum magnetic field.

The concept of piston-driven implosions to stabilize the outer surface of the liner implosion was also developed and demonstrated, with reversible liquid implosions of excellent quality and efficiency provided by axisymmetric piston-drive techniques.[8,9] Two piston-driven liner implosion systems (LINUS-0 and HELIUS) were built and operated to study the behavior of liquid liners at high energy densities. In particular, liquid implosions have been studied for radial compression ratios of $\alpha = 5\text{-}30$ and normalized peak pressures[10] $\zeta = p/\rho c^2 > 0.05$ (where p is the peak pressure in the compressed payload, ρ is the liner mass density, and c is the liner sound speed). Both α and ζ are in the range of interest for a LINUS power reactor.

Piston pressures and implosion speeds of interest for reactors have also been demonstrated in large-scale structures (LINUS-0). In addition, magnetic flux compression has been demonstrated using liquid sodium-potassium in the HELIUS device.[11,12]

The effects of liner compressibility and magnetic flux diffusion have been studied theoretically with numerical codes (WAVER[10] and ADINC) with quite good correlation between theory and experiment. Furthermore, the loss of liner material through open ports in the implosion chamber endwall has been measured experimentally and modeled theoretically. The generation of rotating liquid liners by tangential injection has also been demonstrated experimentally and modeled to provide the basis for calculations of power requirements in reactor level systems.[13]

A more complete review of NRL liner implosion research is provided in References 1 and 14.

III. PRINCIPLES OF LINUS FUSION REACTOR DESIGN

There have been many attempts at fusion reactor design based on imploding liner techniques.[15-23] Two categories can be readily distinguished by the extent to which liner kinetic energy is recovered. If the kinetic energy of the liner implosion is not recovered, the energy for subsequent implosions must be supplied by recirculation of energy from the conversion at modest efficiency (<40%) of reactor output heat to work. This rather inefficient conversion process requires that the nuclear gain (Q) relative to the liner energy must be as high as in other fusion reactor schemes ($Q > 10$) which in turn is reflected in larger and higher energy imploding liner reactors (since at fixed peak pressure the radius scales as Q). The principal problem with this approach is the generation of an uncontrolled explosion subsequent to peak compression which can seriously degrade the frequency of repetition and the economic operation, if not also the safety, of the reactor.

The other category of liner implosion reactor attempts to control the liner energy sufficiently well that a large fraction of the implosion energy is recovered after peak compression. This fraction of the original liner kinetic energy is available to do work against the driving system, thereby replacing energy lost to various inefficiencies (friction, magnetic diffusion, etc.) Much less energy (in principle, zero energy) is then required from processing reactor heat, so the Q-value can be much smaller ($Q \lesssim 2$) without an excessive circulating power fraction ($C \sim 10\%$).

An additional and very important feature associated with the close control of liner kinetic energy is the inherent safety and economy of effort in the reactor operation. The goal is a completely reversible implosion process. Stresses in the system on recovering the liner kinetic energy should not, therefore, be more than experienced during implosion. Restoration of the conditions for liner implosion by means of the controlled recovery of liner kinetic energy (and liner mass) requires liquid liners, thus the requirement for refabrication of solid liners between implosions is removed, and refurbishment of the local apparatus (connections, seals, etc.) can be avoided.

The approach taken by the NRL LINUS program has been to control the liner dynamics closely, thereby planning towards the second category of liner fusion reactor. (Apart from the eventual advantages in a power reactor, an important factor in such planning was the difficulty, if not danger, associated with uncontrolled liner implosions at the increasingly higher energies perceived for research experiments).

The principles of a LINUS fusion reactor design include the distinctions made above and can be stated as follows:

1. Close control of the liner dynamics before and after peak compression by means of rotational stabilization of the inner surface of a liquid liner and by elimination of the free outer surface through use of free-piston drive techniques.
2. Recovery of a significant fraction (>85%) of the implosion

energy to restore the driving system energy with only modest (if any) recirculation of energy from conversion of heat.

3. Use of the liquid liner material as the :

   a) Fusion reactor first-wall

   b) Magnet coil

   c) Reactor blanket (with lithium-bearing liners to breed tritium)

   d) High energy neutron shield

   e) Initial thermal reservoir (for neutron heat and reactions, resistive dissipation, viscous loss, plasma radiation, etc.).

   f) Heat transfer medium

   g) Roughing pump (for high density plasma and impurity vapor associated with peak compression).

Associated with these basic principles are additional important features that are based on experience with pulsed high pressure liner implosion systems that have been built and operated at NRL:

a) Inherent axisymmetry to provide and maintain the quality of the inner liner surface during implosion (since the amplitude of even a stable perturbation relative to the inner surface radius increases approximately as α^2).

b) Inherent simplicity of the solid portions of the drive mechanism and of the mechanical sections, so that stress concentrations are minimized and vibrations/deflections allow close tolerances, smooth operation, and proper, positive dynamic seal action.

c) Provision for reduction of high pressure waves in the liner material ("water hammer") generated at peak compression.

It would also be desirable to obtain the necessary liner rotation for stability by tangential injection of liquid liner material, rather than by rotation of the implosion chamber. The problems of rotary seals, bearing, etc. at high temperature and pressure could thereby be avoided.

Other aspects of a LINUS reactor design depend critically on the plasma/magnetic field configuration to be compressed. At this time, it appears possible that a closed-field system, the so-called <u>compact toroid</u>, will provide a means for preventing the axial escape of hot plasma during liner compression; greatly reducing the necessary axial length of the system. Such a plasma/field system can, in principle, be created in many ways,[4] with present success achieved with θ - and Z-pinch, θ - pinch gun and coaxial plasma gun techniques.

It is likely with these techniques that the initial plasma (or elements thereof) would be created outside the implosion chamber and then transported into the chamber for liner compression. (In any event, energy if not plasma cannot enter the implosion chamber through the liner itself). For program flexibility, in view of the uncertainty of the plasma generation technique at this time, it would be useful for a LINUS reactor design to offer as much access as possible to the implosion chamber; such access would also benefit vacuum conductance.

If plasma gun techniques (Z or θ) allow a plasma to be injected into the chamber, problems of background neutrals may be reduced and magnetic flux could be convected into the chamber. Also, impurity plasma may be displaced from at least the central regions of the D-T plasma by the pulsed entry of the injected plasma/magnetic field. For these reasons, it may be useful to plan, at least tentatively, for plasma and flux to be convected into the implosion chamber through ports in the endwall(s).

Calculations indicate[24] (and experimental evidence tends to confirm[25]) that compact toroids can contract axially during radial compression. This is a useful feature in that such contraction allows the plasma compression to occur faster than simple radial compression. The plasmoid beta value appears to be maintained (instead of dropping as in cylindrical compression) and the stability to many MHD modes improves because the elongation (length to minor radius) increases. For a radial compression of ten, the axial contraction will be about $(10)^{2/5} = 2.5$. An additional benefit of the axial contraction is that it provides a greater distance between the neutron-producing plasma and the ends of the implosion chamber, thereby reducing the neutron flux from an otherwise severe level. (For a semi-infinite line source, starting a distance z_1 from the endwall, the flux relative to that experienced along the liner surface at radius r is $F/F_s = r/2z_1$. Thus $z_1 >> r$ is necessary to avoid neutron fluxes comparable to the very high levels LINUS provides at the liner surface).

It is useful, if not essential, for the liner kinetic energy to converge axially to follow the contraction of the compact toroid. Otherwise, substantial amounts of liner material and energy are not utilized efficiently. To create such axial contraction, it is necessary to provide axial momentum to the liner material from the drive-system. This can be accomplished with two pistons moving axially towards each other, displacing liner material through ducts to generate axial as well as radial liner motion. Since such axial motion automatically directs liner material away from endwall ports, loss of material and energy out the ports provided for plasma access may be substantially reduced.

With the above remarks on basic factors involved in LINUS reactor design, it is useful to consider a possible conceptual arrangement, to examine scaling laws associated with the design and then to calculate a consistent set of operating values. From such an exercise, the potential of LINUS as a reactor may be assessed, and the critical issues for further liner implosion research can be delineated.

IV. AN AXISYMMETRIC LINUS FUSION POWER REACTOR

In Fig. 1, a schematic design is shown of a LINUS fusion power reactor system based on the discussions in Section III. The reactor consists of two oppositely-directed annular pistons driven by high pressure helium and displacing liquid metal both radially and axially. The pistons are arranged to act in a pilot-valve fashion, sealing the drive-gas reservoir or releasing drive-gas to act on the full piston in response to evacuation or pressurization, respectively, of the small volume initially (and finally) just behind the piston (as indicated by the double-headed arrow). The liquid metal is formed continually into a cylindrical liner by tangential injection at the periphery of the liner volume and by axial extraction near the inner surface. For illustrative purposes, a compact toroid plasma is shown injected through an endwall port by a theta-pinch (gun/guidefield) arrangement. A port in the opposite endwall is provided for evacuation of the implosion chamber. The angle of the duct channelling the liner flow and the angle of the piston faces are arranged to provide sufficient axial speed both to follow the axial contraction of the compact toroid and to allow the liner material to return radially beyond the radius of the port before reaching the endwall of the implosion chamber.

The radial and axial compression of the compact toroid increases the plasma temperature and density resulting in a rapid increase in neutron production rate near the time of minimum liner radius. At this time (turn-around), the plasma is surrounded almost completely by a thick layer of liquid metal. Neutrons from the plasma deposit

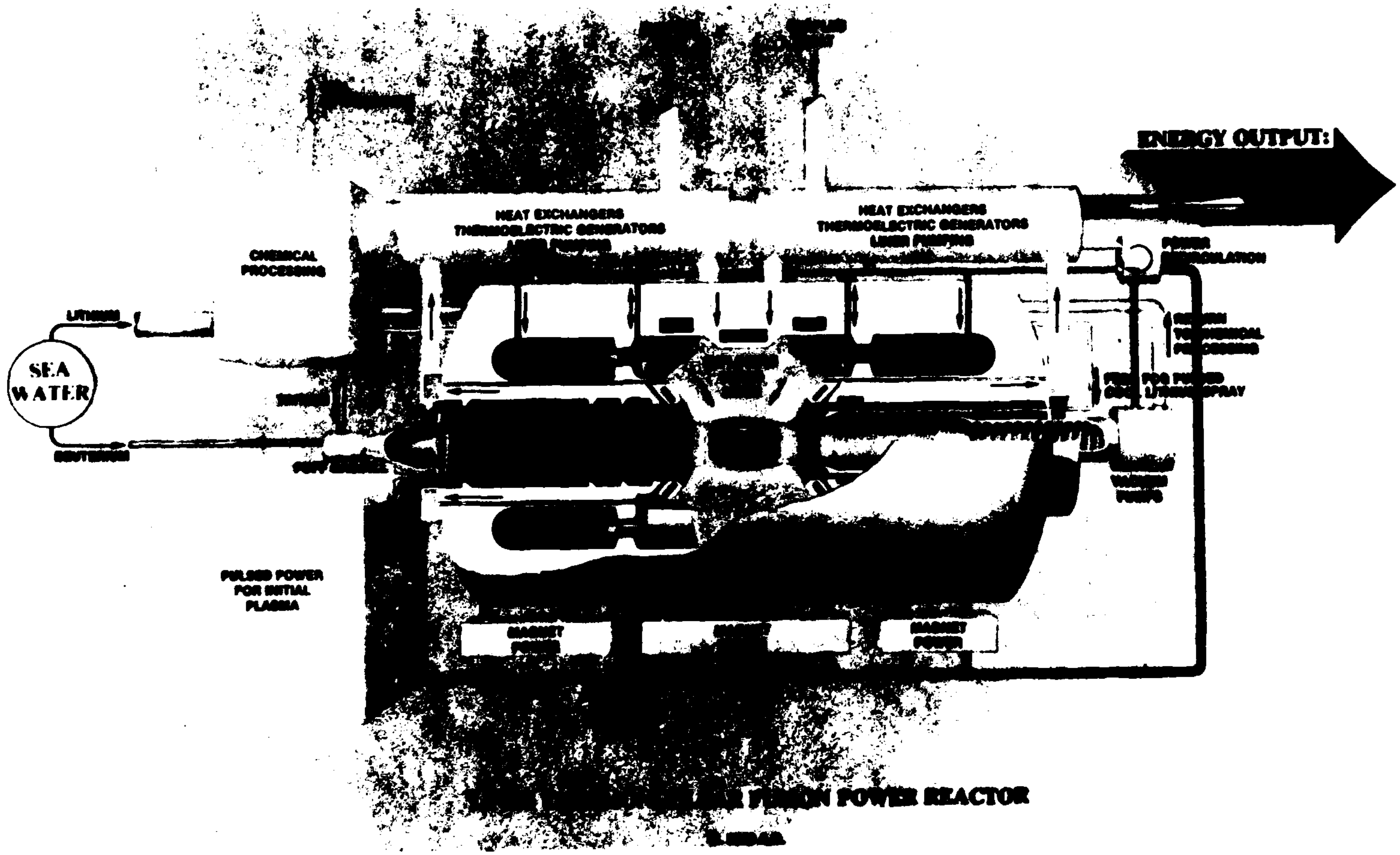


Fig. 1 — LINUS thermonuclear fusion reactor

essentially all of their energy in the liquid liner, so the permanent structure of the reactor is shielded from high energy neutron irradiation. By using lithium-bearing liner material, tritium can be produced in the liner itself, without the requirement for an additional blanket (and the consequent need for a structural interface exposed to high energy neutrons). Tritium is then recovered by chemical processing of the circulating liquid liner flow.

Energy is provided as heat by neutron deposition and nuclear reactions in the liner, resistive dissipation during magnetic flux compression, plasma radiation, and viscous dissipation associated with liner motion. This heat is recovered by circulation of the liner material through heat exchangers and is converted to work by an appropriate thermodynamic cycle. A portion of the work obtained in this way is used by the pumps required to circulate the liner material, by the power system for plasmoid generation and transport, and by systems for vacuum, tritium handling, etc. Some power might also be needed to re-establish the helium driver-gas energy. In principle, however, sufficient additional energy should be obtained from the pressure of fusion alpha-particles on the re-expanding inner surface of the liner, to restore the pressure and energy of the helium drive-gas reservoirs directly by the return motion of the drive pistons. In this way, a portion of the total nuclear energy produced is directly converted to work, allowing operation of a LINUS reactor at reduced Q-values. Such a reduction in Q-values results in smaller reactor dimensions, lower drive-pressure requirements, and more attractive (i.e., lower) net output powers.

The basic operation of this reactor, as with other LINUS reactors, involves two timescales for the flow of liquid metal : 1) continuous flow of liner material, required for transport of heat and mass (tritium extraction, impurity removal, lithium addition, etc.) to maintain average operating conditions such as the liner temperature and composition; and 2) pulsed implosion and re-expansion of the inner surface of the liner to compress the plasma payload, obtain a burst of neutrons, and extract energy from alpha-particle pressure.

The special features of the present design include:

1. Use of only two major moving parts and simple sections which improves mechanical reliability.
2. Tangential injection which eliminates need for rotary seals and bearings. The liner in a sense acts as the bearing fluid.
3. Axial motion of liner material which allows liner energy to follow the contracting plasmoid, permits use of simple ports for plasma injection, and reduces the neutron flux to the end sections.
4. Axial convergence which reduces water hammer effects and pulsed pressure loadings.
5. Simple large ports which provide high conductance for vacuum pumping.
6. Use of puffed gas and plasma injection which reduces problems with background neutrals and provides convection

of magnetic flux into the implosion chamber.

An especially useful feature of the present approach is that important elements of the design are based on experimental results (existing axisymmetric piston-driven implosions, existing closed-magnetic field plasmoid creation techniques, and existing tangentially-injected rotating liners). Additional work, of course, still needs to be done on certain aspects of the system to assess quantitatively the performance of the necessary liner dynamics. Such work will be discussed later.

V. SCALING CONSIDERATIONS FOR LINUS REACTORS

Before deriving a consistent set of operating values for the various dimensions, pressures, temperatures, etc. of a fusion power reactor based on the conceptual design in Fig. 1, it is useful to establish some scaling relationships. Such relationships will primarily indicate the choice of liner material, compression ratio and peak operating magnetic field strength, from which other numbers (drive pressure, size, output power) can be determined.

For example, it would seem advantageous to operate at the highest possible magnetic field level, since the peak plasma density would then be highest, the required confinement time lowest and the reactor size and energy least. Material compressibility of the liquid liner, however, results in less energy in the plasma payload as the peak pressure loading on the inner surface of the liner increases, and more of the system energy is absorbed in compressing the liner itself.

To determine properly the optimum reactor operating magnetic field, calculation of the dynamics of the compressible metal liner is required. Such calculations have been performed[10] for rotationally-stabilized liner implosions and have been used to compute the efficiency of transferring total system energy E_T to payload energy E_p, and the nuclear energy gain, Q , relative to E_T . The output of numerical calculations is obtained as two functions of the dimensionless parameters α and ζ defined below:

$$E_p/E_T = \varepsilon(\alpha,\zeta)$$

and

$$\frac{Q}{\rho c r_f} = P(\alpha,\zeta)F(\beta)$$

where α is the radial compression ratio, r_f is the minimum radius of the liner surface, ρ and c are the liner mass density and sound speed, respectively, and $\zeta = p_f/\rho c^2$ is the ratio of peak payload pressure p_f to the characteristic dynamic pressure of the liner material, ρc^2. The actual calculations consider compression of a uniform, field-free plasma. The thermonuclear gain achieved in compressing a field-plasma mixture is obtained by multiplying $P(\alpha,\zeta)$ by a function $F(\beta)$ which characterizes a finite beta plasmoid near peak compression. Since β remains nearly constant during liner compression of a compact toroid, adjusting $P(\alpha,\zeta)$ with a constant value $F(\beta)$ should be reasonably accurate.

The minimum radius r_f is thus:

$$r_f = \frac{Q}{\rho c P(\alpha,\zeta)F(\beta)}$$

The initial radius of the inner surface is αr_f, so the total radius of the reactor (implosion system) vessel may be written as:

$$r_T = (\alpha r_f)\, g\, (1 + p_D/S)$$

with g, a geometric design factor, S, the allowable mechanical stress in the vessel, and p_D is the drive-pressure. (The use of a simple thin-walled structure formula $\Delta r/r = p/S$ is convenient for initial design purposes and is accurate to 25% for $p/S \lesssim 0.3$).

The relationship of drive-pressure to payload pressure in terms of conservation of energy can be used to calculate r_T, but must allow for the axial contraction of the plasmoid. The length ℓ of an axially contracting compact toroid scales approximately as:

$$\ell = \ell_o \alpha^{-2/5} \ .$$

If the work done in displacing liner material is equated to the energy required to compress such a plasmoid (with a rotationally-stabilized implosion):

$$p_D \pi r_o^2 \left\{ \alpha^{2/5} \left[1 - \frac{(1-\alpha^{-2/5})}{3} \right] \right\} = \frac{p_f}{(\gamma-1)} \ \frac{\pi r_f^2}{\varepsilon(\alpha,\zeta)} \ ,$$

then the drive pressure will be approximately:

$$p_D = \frac{\rho c^2}{(\gamma-1)} \ \frac{\zeta}{\varepsilon} \ \frac{1}{\alpha^{12/5} \ 1-(1-\alpha^{-2/5})/3}$$

If $p_D/\rho c^2$ is specified, then $\zeta = \zeta(\alpha; p_D/\rho c^2)$ can be obtained. The efficiency $\varepsilon(\alpha,\zeta)$ and nuclear gain function $P(\alpha,\zeta)$ can then also be determined by choice of α. The total reactor radius r_T may then be found in terms of α and the necessary Q-value:

$$r_T = \frac{\alpha Q \varepsilon (1+p_D/S)}{\rho c F(\beta) P(\alpha)}$$

The necessary value of Q can be obtained from the condition that the alpha-particle energy compensate for losses during the implosion-reexpansion cycle of the liner:

$$f_\alpha Q E_T = f_m E_T + f_D E_T \varepsilon(\alpha)$$

where f_α = Fraction of nuclear energy in alphas ($\cong 0.156$)

f_m = Fraction of liner energy lost during cycle ($\cong 0.15$ from experiments)

and f_D = Fraction of payload energy εE_T lost to diffusion (resistance, radiation, particle loss, or other processes that occur on the scale of the compressed plasmoid). For diffusion loss, $f_D = 2\delta/r_f$, where δ is a characteristic skin-depth for energy loss.

If magnetic diffusion into the liner surface is the principal loss, then $\delta = k_D (\eta\tau/\mu)^{1/2}$, where η is the liner resistivity; and τ is the pulse time of the compressed magnetic field which can be scaled to the nuclear burn time τ_{nuc} required to achieve the necessary Q-value:

$$\tau = k_b \tau_{nuc} = \frac{k_b Q L(T)}{\zeta \rho c^2 \varepsilon(\alpha)}$$

where L(T) is a function of plasma temperature which relates the gain in nuclear energy (relative to plasma energy) to the peak plasma density, n :

$$n\tau_{nuc} = \frac{QL(T)}{2kT} .$$

By substitution and rearrangement, the equation for Q is:

$$Q^{3/2} = \frac{f_m}{f_\alpha} Q^{\frac{1}{2}} + 2\frac{k_D}{f_\alpha} (k_b L(T) \frac{\eta\rho}{\mu})^{\frac{1}{2}} F(\beta)P(\alpha) (\frac{\epsilon}{\zeta})^{\frac{1}{2}}$$

Since it can happen that $f_m \cong f_\alpha$, the equation simplifies to:

$$Q^{3/2} = Q^{\frac{1}{2}} + K(\alpha;\rho,\eta, \text{etc.}) .$$

If $K << 1$, and $Q \approx 1 + \Delta$, then

$$Q \approx 1 + \frac{2k_D}{f_\alpha} (\frac{k_b L(T)}{\mu})^{\frac{1}{2}} F(\beta)P(\alpha) (\frac{\epsilon}{\zeta})^{\frac{1}{2}} \eta^{\frac{1}{2}} \rho^{\frac{1}{2}} .$$

With a mechanical loss factor $f_m \cong 0.15$, Q is not very sensitive to ρ or η . In Table I, values of Q and $\alpha r_f F(\beta)$ are displayed for different choices of α and liner material. (Parameter values used here include: p_D = 3000 psi, S = 15,000 psi, η = 35μΩ-cm for lithium and for lead-lithium, η = 103 μΩ-cm) . From this table, it is seen that to achieve restoration of the drive-system energy, without recirculation of any power from the reactor heat output, the necessary Q-values are less than 1.6 for all choices shown. For a given final plasma configuration ($F(\beta)$), the initial inner surface radius decreases with lower values of α and is less for higher mass density liners.

To examine the economic advantages, if any, for different parameter choices a rough cost estimate can be made. By adding the cost of reactor fabrication (taken as proportional to the weight W_R of a solid cylinder of radius r_T and length $\ell_T = \lambda \ell_o$) and the cost of the plasmoid energy (taken simply as proportional to the initial plasmoid energy E_{pi}), the total reactor cost is:

$$\$ = K_R W_R + K_p E_{pi} \quad .$$

The cost per kilowatt (thermal) is then:

$$\Sigma = \frac{(\gamma-1)\epsilon}{\nu Q \zeta \alpha^{3/5}} \; \frac{g^2 \lambda (1+p_D/S)^2 K_R \alpha^3}{\rho c^2} + \frac{K_p \zeta \alpha^{-1.6}}{(\gamma-1)}$$

where ν is the effective (compressional) specific heat ratio for the compact toroid ($\gamma \approx 1.8$), and ν is the repetition frequency of the implosion-reexpansion cycle. In Table II, $\nu\Sigma$ and $r_T F(\beta)$ are displayed for parameters as before (and with $g = 2.17$, $\lambda = 4.24$ taken from Fig. 1 ; it is also assumed that $K_R = 10\$/\mathrm{lb}$ for a steel fabrication, and $K_p = 1.0$ \$/joule). Higher mass density liners are favored again in terms of smaller system sizes and lower cost/kW. (The crudity of the cost estimate, however, provides . rather larger error bar, so cost values and trends should be taken only as suggestive).

Note that $\nu\Sigma$ is independent of $F(\beta)$, except through the Q-value, indicating that the running costs ($/kW) of the reactor would not be sensitive to the plasma/magnetic field profile. The initial capital expenditures and siting costs, however, will depend on the actual thermal power, which may be written as:

$$P_H = \frac{\pi \Lambda \nu}{(\gamma-1)} \frac{Q^4}{F^3(\beta)} \frac{\alpha^{3/5}}{(\epsilon/\zeta)P(\alpha)} \frac{1}{\rho^2 c}$$

where $\Lambda = \ell_o/r_o$ is the initial length-to-radius ratio of the plasmoid.

Values for $P_H F^3(\beta)/\nu$ are given in Table III (For $\Lambda = 6$) . Again, heavy liners are favored, much more strongly now than in Table II. Note also the strong dependence of P_H on $F(\beta)$, and thus on the final plasma/field profile. This behavior reflects cubically the increase in system radius (at fixed energy density) needed to obtain the necessary Q-value with less productive plasma payloads.

To compute the actual output power requires the determination of the repetition frequency ν . By the previous specification of Q, power is not required to maintain the implosion-reexpansion cycle itself. Power is needed, however, to maintain the flow of liquid metal through the reactor system, including tangential injection for liner rotation. The repetition frequency must be such that the steady power requirements are only a small fraction C of the power produced by processing the reactor heat into useful work. Typically, the circulating power fraction, C , is required to be less than 15% (although only a complete financial analysis can really specify C).

The steady power requirement due to liner transport and circulation has two main elements: power required in the implosion chamber to sustain liner rotation for stability at peak compression; and power expended in flow through external piping, heat exchangers, etc. that will be proportional to the volumetric flow of liner material associated with heat and tritium removal, and with the tangential-injection flow requirements. The power dissipated (by wall shear) in the implosion chamber for liner rotation may be written as:

$$P_R = K_{ROT} \rho \mu_\theta^3 \ell_o r_o$$

where $u_\theta \approx \left(\frac{\gamma-1}{\gamma}\right)^{1/2} \left(\frac{p_D}{\rho}\right)^{1/2} \frac{1}{\ell n^{1/2} g\alpha}$ is the tangential speed of the flow at the initial inner surface of the liner, and K_{ROT} is a constant determined by modeling the NRL tangential-injection experiment; scaled to account for differences in Reynolds number and roughness factor, $K_{ROT} \approx 0.02$. If a repetition rate ν_R is defined as that which results in a circulating power fraction C_R then:

$$\frac{\epsilon_H C_R \nu_R}{F(\beta)} = \frac{K_{ROT}}{\pi} \frac{(\gamma-1)^{5/2}}{\gamma^{3/2}} \frac{p_D^{3/2} \alpha^{7/5} P(\alpha)(\epsilon/\zeta)}{Q^2 \rho^{1/2} c \; \ell n^{3/2} g \; \alpha}$$

where ϵ_H is the efficiency of conversion of heat to work. With $\nu_R \approx F(\beta)$, the output power $(P_H/\nu)\nu_R$ scales inversely with $F^2(\beta)$. Since ν_R varies only by about a factor of 2.6 over the parameter choices previously used, the lowest power output still appears to be associated with heavier liners and lower compression ratios.

The power loss due to liner transport in the external plumbing must also be evaluated. This loss may be written as:

$$P_{Tr} = K_{Tr}\rho u^2\dot{V} = K_{Tr}\,\rho\,\frac{\dot{V}^3}{A^2}$$

where $\dot{V}$ = Volume flow rate

= uA, with A, the duct area

and K_{Tr} = Constant based on Reynolds number, roughness factor, and the length-to-diameter ratio of the piping. The volume flow rate depends on two factors: 1) the requirement for limited excursions in the mean flow temperature,

$$\dot{V}_H = P_H/C_v\Delta T\ ,$$

where C_v is the specific heat per unit volume of the liner material and ΔT is the allowable temperature rise in passing through the reactor; and 2) the volume flow associated with the required rotational speed

$$\dot{V}_R = u_\theta G(\ell_o\ ,\ r_o)$$

where $G(\ell_o\ ,\ r_o)$ is a geometric factor based, for example, on the tangential-injection experiment. For Pb-Li , $\dot{V}_H$ and $\dot{V}_R$ are comparable; while for L_i , $\dot{V}_R > \dot{V}_H$. Power requirements for liner transport, therefore, do not depend strongly on C_v , but merely on ρ/A^2 , and K_{TR} . For a given duct, K_{TR} decreases slightly with ρ . Heat transfer systems, however, typically favor lower mass density, higher thermal conductivity fluids. A complete analysis of the liner transport, including in particular the tritium extraction system, needs to be performed to achieve more confidence in design choices and trade-offs.

An additional amount of recirculating power in reactor operation is that required by the plasmoid generation and transport system. The peak plasmoid energy is:

$$E_{pf} = \frac{\rho c^2 \zeta}{(\gamma-1)} \pi r_f^2 \ell_f$$

$$= \frac{\pi \Lambda}{(\gamma-1)} \frac{Q^3 \zeta \alpha^{3/5}}{\rho^2 c F^3(\beta) P^3(\alpha)} .$$

The initial plasmoid energy just prior to compression is therefore:

$$E_{Pi} \cong E_{pf} \alpha^{-1.6}$$

$$= \frac{\pi \Lambda}{(\gamma-1)} \frac{Q^3}{\rho^2 c F^3(\beta)} \frac{(\zeta/\alpha)}{P^3(\alpha)} .$$

The power required by the plasmoid generation system is then:

$$P_p = \frac{E_{pi}}{\epsilon_p}(1-f_s)\nu \quad ,$$

where ϵ_p in the efficiency of the plasmoid generation and transport process and f_s is the fraction of the system energy that can be regained electrically for use in generating subsequent plasmoids.

In terms of scaling, it is readily noted that the initial plasmoid energy (plasma and magnetic field) necessary to achieve the required Q-value increases rapidly as $F(\beta)$ decreases; it also decreases approximately linearly with α (since $P(\alpha)$ is a rather weak function of α). Higher mass density liners are favored again, as are lower aspect ratio ($\Lambda = \ell_o/r_o$) plasmoids. Note also that the initial plasmoid temperature required to achieve a desired peak final temperature scales as $T_i = T_f\alpha^{-1.6}$, so higher values of α may be necessary depending on the success of plasma generation research.

VI. SAMPLE DESIGN VALUES

The scaling relationships displayed in the preceding section allow a self-consistent set of parameter values to be selected for a LINUS power reactor. Such a set of values is shown in Table IV. It should, of course, be noted that these values are representative and cannot be considered definitive until several efforts in plasma physics and liner technology are completed. In particular, the following sources of uncertainty can be readily identified for further work:

1. Calculations for liner dynamics and thermonuclear energy gain are based on a one-dimensional compressible fluid code computations of rotating liquid metal liners compressing uniform field-free plasmas. These computations are connected algebraically to the quasi- two dimensional liner flow and compact toroid plasma compression in terms of overall energy balances. An actual set of two-dimensional calculations would clearly be more accurate.

The figures in Table IV have been computed with various parameter choices and/or consequences that should also be noted. For example, the value of $F(\beta) = 0.3$ corresponds approximately to the pressure-averaged beta value of about 0.55 previously computed[24] for compact toroids. The compressed plasma temperature of 15 keV is probably lower than the peak of the temperature distribution in the compact toroid for an optimum situation.

The drive pressure of $p_D = 3000$ psi would combine with a stress level of 15,000 psi in the reactor vessel to require a wall thickness

20% greater than the inner vessel radius. Such a relative wall thickness, corresponding to 85 cm for $\alpha = 10$ and $g = 2.2$, is not excessive for the use of thin-walled structure equations. Fabrication techniques for the reactor vessel could follow the wrapping techniques used in large naval pressure vessels or parallel flat-plate constructions as used in Suzy II and LINUS-0; in any event, transmission-inspection of material for flaws would not necessarily be required through the final radial thickness.

The various power levels indicated for plasmoid generation, liner rotation and liner transport also involve several specific assumptions. For example, in the plasmoid generation system, an efficiency of 20% is assumed; but it is also required that half of the electrical energy needed to create and transport the plasmoid can be recovered without passing through the thermoelectric system. The liner rotation power is calculated by scaling up the NRL tangential-injection experiment, including in the scale-up the change in Reynolds number and roughness factor. That is, the power required for rotation may be written as

$$P_{ROT} = K_{ROT}\, \rho u_{\theta}^{3} \ell_{o} r_{o}$$

where u_{θ} is the tangential speed of the inner surface provided by the free-vortex flow of the injection and exhaust system. The necessary value of u_{θ} can be estimated from the rotational energy required by a cylindrical free-vortex implosion operating at low

ζ value:

$$\pi\rho u_{\theta o}^2 r_o^2 \ln g\alpha = \left(\frac{\gamma-1}{\gamma}\right) p_D \pi r_o^2$$

so

$$u_{\theta o} = \left(\frac{\gamma-1}{\gamma}\right)^{\frac{1}{2}} \left(\frac{p_D}{\rho}\right)^{\frac{1}{2}} \frac{1}{\ln^{\frac{1}{2}} g\alpha} \quad , \quad \text{as quoted earlier.}$$

For the conditions of the sample design, $u_\theta = 16.7$ m/sec . The equivalent speed in the tangential-injection experiment is based on the free-vortex portion of the fluid field (since in the reactor it would be desirable to exhaust the flow as close to the heated inner surface as possible). In the experiment, $u_\theta = 6.3$ m/s. The Reynolds number and roughness factor for the experiment are Rey = 11,500 and $\varepsilon/D = 0.002$, respectively, providing a Darcy-Welsbach friction coefficient of $f = 0.033$. In the reactor, Rey $= 8.9 \times 10^6$, and $\varepsilon/D = 4.3 \times 10^{-5}$, for which $f = 0.011$. The flow constant K_{ROT} is proportional to f , so an appropriate adjustment must be included in scaling the rotational power requirement for the reactor from the NRL experiment. It is also important to recognize that only the wall shear loss in the experiment should be extrapolated since no attempt had been made in the experimental apparatus to match the inlet and outlet port geometries to the swirling flow; considerable reduction in losses associated with these ports can be expected. With the relative values of speed, friction coefficient, and system size, the extrapolated wall-shear power loss is 11.5 MW(e) . An additional 60% has been included in the number in

Table IV to allow for other losses, presuming that at least a factor of two improvement is possible by proper inlet and outlet port design.

The volume flow rate of liner material through the reactor vessel can also be scaled from the NRL experiment since $\dot{V} \approx u_\theta \ell_o r_o$. In the experiment, $\dot{V}$ = 3.3 ℓ/sec , so in the sample reactor design, $\dot{V}$ = 19 m^3/sec. With a heat capacity per unit volume for the liner material of 1.76 x 10^6 J/m^3-^{o}C , and a thermal power input of P_H = 1790 MW , the mean temperature rise will be

$$\Delta T = \frac{P_H}{c_r \dot{V}} = 53\ ^oC$$

This should be an acceptable value (even ΔT = 100 ^{o}C might be satisfactory) for system operation. Note that the volume flow rate to achieve an acceptable temperature excursion is thus about the same as required for rotational stabilization. The level of turbulence in the reactor should therefore also be about the same even if tangential-injection is not used to create liner rotation. Thus, tangential-injection does not by itself introduce turbulence and should not be faulted on the basis of increased viscosity due to turbulence.

To estimate the power losses associated with transporting the liner material at the flow rate $\dot{V}$ given above, it is necessary to provide at least a rough design of the piping system. The principal constraint is the exhaust flow conduits leading from the reactor vessel. From the schematic in Fig. 1, it may be estimated that a total cross-section of 11 m^2 is available on each side for the duct structure needed to exhaust the liner flow. If twenty per cent of this area is the exhaust port area, divided into four channels on each side, then the effective hydraulic diameter for the channels will be about D = 0.8 m , and the exhaust speed of the flow for $\dot{V}$ = 19 m^3/s will be $u = 4.3$ m/s. The Reynolds number for this flow will then be $Rey = 2.1 \times 10^7$. With a roughness value of 5.7×10^{-5}, the friction coefficient is $f \cong 0.01$ and the power loss in the channels is:

$$P_{TRAN} = \frac{f}{2} \rho u^2 \dot{V} \left(\frac{\ell}{D}\right) = 18.6 \text{ kW(e) } \left(\frac{\ell}{D}\right)$$

where ℓ is the length of the channels. For $P_{TRAN} = 2.8$ MW(e), $(\ell/D) = 150$, so each channel can be 120 m. long. Again, the actual power loss associated with liner transport must be based on a complete power station design including tritium handling. The above results are merely to illustrate that quite substantial channel lengths can be tolerated at modest fractions of recirculated power. To provide some margin for further design effort, the table of reactor design values quotes an allowed circulating power fraction of 15%, even though a circulating power fraction of 10% is computed, (representing a minimum value which might survive more detailed analyses.)

VII. CONCLUDING DISCUSSION

The sample design for a LINUS fusion power reactor presented in the preceding section provides a quantitative indication that the conceptual design shown in Fig. 1 can be realized in practice. The benefits of such a conceptual design have already been mentioned and include mechanical simplicity (two main moving parts, no rotary seals, bearings, etc.) and design flexibility to match with different candidate sources for the initial plasma. Several aspects of the design, however, have not been explored fully and should bear consideration in future work. These include:

1) The basic two-dimensional implosion of a turbulent rotating liquid liner should be demonstrated and analysed by the simple hydrodynamic-experimental techniques that have proven so successful previously in the LINUS program. In particular, the quality of the inner surface and the efficiency of recovering energy into stored gas energy should be assessed. It should be possible to build and operate a system which uses the piston as the pilot-valve as in the reactor schematic, thereby allowing even the valving portion of the device to be examined.

2) The axial convergence of the liner flow, as required to follow a contracting compact toroid, is also utilized to reduce loss of material from the endwall ports. The same apparatus, as in Item 1, can and should be used to establish techniques for guiding the flow to eliminate endloss. The efficiency of recovery of liner material is critical to power reactor operation. Pulsed pressure waves (water hammers) are also important in the reactor design (precluding complex port mechanisms, for example) and should be reduced by the quasi-spherical implosions associated

with axial convergence.

3) The presence of high atomic number impurities in the implosion volume due to the necessarily high operating temperature of the reactor working fluid may prevent thermonuclear conditions from being achieved. Portions of the liner near the inner surface will be heated the most due to neutrons, plasma radiation, magnetic diffusion, and viscosity. The present design provides for turbulent mixing and preferential removal of these portions of the liner. The schematic also shows a separate injector for lithium which could spray the inner surface of the liner with cooler, low z material just prior to plasma injection. Reduction of the impurity level is achieved by such cooling and is assisted by the expansion of vapor created near turnaround by the re-expansion of the liner; also good vacuum conductance to cold plates (shown as circular ridges in the end opposite the plasma injector) helps to reduce impurity vapors. A primary technique for keeping impurities out of the plasmoid may be the plasma injection process itself which should sweep impurity ions to the periphery of the plasmoid as it enters the implosion chamber. Questions of impurity vapor and its effect on plasma performance can best be addressed, in the near term, in plasma experiments which provide the necessary energy density to ionize impurities. Resistive heating of the liner surface will certainly provide impurity vapor, but does not allow any significant mitigation of the effects of such vapor due to ionization in the magnetic field.

At this time, additional information is still required on candidate plasma systems and on the behavior of two dimensional liner implosions, before definitive designs can be achieved for a LINUS reactor. It appears, however, that substantial development of compact toroid plasmas will occur in the next two years. If liner implosion systems modeled along the lines of the reactor design discussed here can be operated and analysed, in this same time frame, rapid progress toward an imploding liner fusion reactor should be possible.

Table I — Variation of Q and $\alpha r_f F(B)$ with α for lithium and lead-lithium liners with P_D = 3,000 psi and S = 15,000 psi

	α	Q	$\alpha r_f F(\beta)$
Lithium	10	1.09	2.28
	20	1.03	4.2
Pb-Li	10	1.52	0.56
	20	1.25	0.89

Table II — Variation of $\nu\Sigma$ and $r_T F(B)$ with α for lithium and lead-lithium liners with P_D = 3,000 psi and S = 15,000 psi

	α	$r_T F(\beta)$	$\nu\Sigma$ (\$/kW (THERM))
Lithium	10	5.9	335
	20	11	335
Pb-Li	10	1.5	249
	20	2.3	279

Table III — Variation of $P_H F^3(B)/\nu$ with α for lithium and lead-lithium liners with Λ = 6, P_D = 3,000 psi and S = 15,000 psi

	α	$F^3(\beta) P_H/\nu$ (GW-sec)
Lithium	10	2.6
	20	15.3
Pb-Li	10	0.06
	20	0.17

Table IV — Sample LINUS reactor design

DESIGN CHOICES

Liner Material: Pb-Li ,

Compression Ratio: $\alpha = 10$

Compressed Plasma Temperature: $T = 15$ keV

Reaction Profile Parameter: $F(\beta) = 0.3$

Drive Pressure: $p_D = 3000$ psi

DERIVED VALUES

Compressed Field: $B = 0.54$ MG

Operating Q-Value: $Q = 1.55$

Initial Plasma Temperature: $T_i = 377$ eV (446)

Initial Plasma Radius: $r_o = 1.9$ m

Initial Plasma Length: $\ell_o = 7.8$ m

Compressed Plasma Length: $\ell = 3.1$ m

Initial Plasmoid Energy: $E_i = 13$ MJ (15.4)

Plasmoid Supply Energy: $E_s = 66$ MJ (78)

CALCULATED PERFORMANCE:

Output Thermal Power: $P_H = 1790$ MW(H) @ $\upsilon = 1$ Hz

Liner Rotation Power: $P_R = 19.1$ MW(e)

Liner Transport Power: $P_{TR} = 2.8$ MW(e)

Plasmoid Source Power: $P_p = 33$ MW(e), (39)

Total Electric Power: $P_T = 597$ MW(e) @ $\epsilon_H = 0.33$

Minimum Circ. Fraction: $C_m = 9.2\%$, (10.2)

Allowed Circ. Fraction: $C = 15\%$

Net Electric Power: $P_N = 507$ MW(e)

Total Reactor Radius: $r_T = 5.1$ m

Cost Per kW (Thermal): $\Sigma = 148$ \$/kW(H)

Total Cost: \$ = 265 M\$

Numbers in Parentheses ( ) are Corrections for Lower Compression of Plasma if Full Diffusion is Assumed.

Appendix I

Estimation of the Duct Angle Required for Recovery of the Liner Flow

The flow of liner material near the edges of the axially-converging liner implosion represents a two-dimensional, unsteady free-boundary problem and probably will require numerical computations and/or experiments for proper solution. An estimate of the flow may be attempted, however, by simplifying the actual pressure field conditions. Suppose that at an axial station far from the ends the liner flow can be treated as a simple, freely-collapsing cylinder so the radial speed at any point is given by

$$u_r = \frac{u_{ro} r_o}{r}$$

where u_{ro} is the radial speed at the radius r_o. It will be further presumed that u_{ro} is a constant (as if the pistons were displacing volume at a uniform rate). The basic momentum equation of fluid elements at this axial station will then be:

$$\frac{du_r}{dt} = -\frac{1}{\rho}\frac{dP}{dr} = -\left(\frac{u_{ro} r_o}{r^2}\right) u_r$$

$$= -\frac{u_r^2}{r}$$

where rotational effects have been ignored. If the pressure on the front surface is also neglected, then the pressure within the liner as a function of radius r can be written as:

$$P = \frac{1}{2} \rho u^2_{ro} r^2_o \left(\frac{1}{r_1^2} - \frac{1}{r^2} \right)$$

where r_1 is the inner surface radius.

The axial acceleration of a fluid element near the ends of the liner can then be estimated by approximating the actual pressure gradient by the pressure (as a function of radius) computed above, divided by the axial distance z of the fluid element from the midplane of the liner implosion:

$$\frac{du_z}{dt} = - \frac{1}{\rho} \frac{dP}{dz} \cong - \frac{1}{\rho} \frac{P(r)}{z} \quad .$$

The axial acceleration can then be written in terms of the radial acceleration:

$$\frac{du_z}{dt} = \frac{1}{2} \left(\frac{r}{z} \right) \left[\left(\frac{r}{r_1} \right)^2 - 1 \right] \frac{du_r}{dt} \quad .$$

If the quasi-cylindrical implosion approximation is maintained, then it is reasonable to consider z as a constant and substitute for du_r/dt as before. The total change Δu_z in the axial speed of a fluid particle from the time it leaves the radial position r_o until it reaches a smaller radius r is then obtained by integration:

$$\Delta u_z = - \frac{1}{2} \frac{u^2_{ro} r^2_o}{z} \int_o^t \left[\frac{1}{r_1^2} - \frac{1}{r^2} \right] dt$$

$$= \frac{1}{4} \frac{u_{ro} r_o}{z} \ln \left[\frac{r^2_o - s^2}{r^2 - s^2} \cdot \frac{r^2}{r_o^2} \right]$$

where $s^2 = r^2 - r_1^2$ is a lagrangian parameter labelling the fluid particle. The maximum value of Δu_z occurs at the time of minimum liner radius, for which $r_1 = r_f$ and $s^2 = r^2 - r_f^2$, and is experienced by the fluid particle with $r^2 = (r_o^2 + r_f^2)/2$. The maximum value of Δu_z is then:

$$\Delta u_{zm} = \frac{1}{2} \frac{u_{ro} r_o}{z} \ln \left(\frac{\alpha^2 + 1}{2\alpha} \right)$$

where $\alpha = r_o/r_f$ is the radial compression ratio of the inner surface. Since the calculation of the pressure field is symmetric with respect to the implosion-reexpansion cycle, the combined axial velocity increment for a fluid element before returning to radius r_o is merely $\Delta u_T = 2\Delta u_z$. For the maximum increment,

$$\Delta u_{TM} = \frac{u_{ro} r_o}{z} \ln \left(\frac{\alpha^2 + 1}{2\alpha} \right) .$$

Suppose, as an extreme condition, it is required that the maximum axial velocity increment is just balanced by the initial axial speed u_{zo} provided to the liner material:

$$u_{zo} = \frac{u_{ro} r_o}{z} \ln \left(\frac{\alpha^2 + 1}{2\alpha} \right) ,$$

then

$$\frac{u_{zo}}{u_{ro}} = \frac{r_o}{z} \ln \left(\frac{\alpha^2 + 1}{2\alpha} \right) .$$

For a constant liner flow, the preceding ratio gives an estimate of the tangent of the necessary duct angle relative to the simple radial motion. For $\alpha = 10$, $r_o = 1.9$ m , $z = \ell/2 = 3.9$ m , this angle is 38^o . (With axial contraction, z may decrease by at most a factor of 2.5 , for which the necessary angle becomes 63^o ; an average value of 50^o is shown in Fig. 1).

Note that in the above calculation compressibility effects, rotation, and payload pressure effects have been ignored. The restraining influence of magnetic fields due to currents induced in the liner have, however, also been neglected. Furthermore, the axial velocity of the liner can reverse and still allow capture depending on the detailed trajectories of the fluid particles. The preceding analysis is clearly approximate and is intended only to provide a plausible reason for attempting more accurate calculations and actual experimental tests.

References

1. P.J. Turchi, A.L. Cooper, R.D. Ford, D.J. Jenkins, and R.L. Burton, "Review of the NRL Liner Implosion Program" in Megagauss Physics and Technology," P.J. Turchi, ed., Plenum Press, N.Y. (1980), p. 375.
2. C.M. Fowler, in Atomic Energy Research in the Life and Physical Sciences, 1960, (AEC Special Report, Jan. 1961), p. 104.
3. J.G. Linhart, H.Knoepfel, C. Gourlan, Nucl. Fusion 1962 Supplement 2: 733.
4. Proceedings of US-Japan Symp. on Compact Toruses and Energetic Particle Injection, Princeton, (Dec. 1979).
5. P.J. Turchi, NRL Memo Report 3218 (February 1976).
6. P.J. Turchi, et al, Phys. Rev. Lett. 36: 1546 (1976).
7. A. Baricilon, D.L. Book, and A.L. Cooper, Phys. Fluids 17:1707 (1974).
8. P.J. Turchi, et al, NRL Memo Report 3511 (June 1977).
9. R.L. Burton, et al, in Proc. of 7th Symposium on Engineering Problems of Fusion Research (IEEE Pub. No. 77CH1267-4NPS) p. 225 (1977). Also P.J. Turchi, D.L. Book, R.L. Burton, A.L. Cooper, J. Mag and Mag. Matls., 11 (1979) p. 372.
10. D.L. Book and P.J. Turchi, Phys. Fluids 22:68 (1979).
11. P.J. Turchi, A.L. Cooper, D.J. Jenkins, E.P. Scannell, Bull. Am. Phys. Soc., Nov. 1979.
12. P.J. Turchi, A.L. Cooper, D.J. Jenkins, E.P. Scannell, IEEE Conf. on Plasma Science,May 1980, Paper 4A3.
13. R.L. Burton, et al, NRL Memo Report 4025 (June 1979).
14. P.J. Turchi, et al, NRL Memo Report 4092 (Sept 1979).
15. E.P. Velikhov, Zh. Teor. Fiz. Series 2, 43:429 (1973).

16. P.J. Turchi, NRL Memo Report 2711 (Jan 1974).

17. P.J. Turchi, Zh. Prik. Mek. Tek. Fiz. 4:32 (1975). In English as NRL Memo Report 3094 (Aug 1975).

18. O.A. Gusev, et al , in Conf. on Engineering Problems of Thermonuclear Reactors, NIIEFA, Leningrad, (1977) p. 74.

19. R.W. Moses, et al, LASL Report LA-UR-78-2374.

20. R.L. Burton and P.J. Turchi, Proc. 13th Intersoc. Energy Conversion Engineering Conf., 1978 (Paper 17NFU3).

21. A.E. Robson, in Megagauss Physics and Technology, P.J. Turchi, ed., Plenum Press, N.Y. (1980), p. 425.

22. R.A. Krakowski, et al, in Plasma Physics and Controlled Nuclear Fusion Research (IAEA Conf., Brussels 1980), Paper CN-38.

23. R.L. Miller and R.A. Krakowski, US DOE Report (to be published, 1980).

24. W. Grossmann and J. Saltzman, in Megagauss Physics and Technology, P.J. Turchi, ed., Plenum Press, N.Y. (1980), p. 403.

25. R. Linford, Private Communication.

DISTRIBUTION LIST

Los Alamos Scientific Laboratory
P.O. Box 1663
Los Alamos, NMex 87545

Attn:	R. Krakowski (MS 641)	1 copy
	R. L. Miller (MX 641)	1 copy
	C. M. Fowler (MS 970)	1 copy
	H. Dreicer (CTR-DO)	1 copy

Sandia Laboratories
P.O. Box 5800
Albuquerque, NMex 87115

Attn:	M. Cowan (DEPT 4230)	1 copy

DOE/ETM
Department of Energy
Washington, D. C. 20545

Attn:	W. Dove	1 copy
	W. R. Ellis	1 copy
	J. Decker	1 copy

Department of Mechanical Engineering
University of Texas
Austin, Texas 78712

Attn:	Prof. H. G. Rylander	1 copy

Electric Power Research Institute
P.O. Box 10412
Palo Alto, California 94303

Attn:	Dr. Noel Amherd	1 copy

Mathematic Sciences Northwest, Inc.
P.O. Box 1887
Bellevue, Washington 98009

Attn:	G. Vuazez	1 copy
	A. Hoffman	1 copy
	T. Churchill	1 copy
	P. Rose	1 copy

I. V. Kurchatov Institute of Atomic Energy
46 Ulitsa Kurchatov, P.O. Box 3402
Moscow. USSR

Attn:	E. P. Velikhov	1 copy
	S. G. Alikhanov	1 copy
	R. H. Kurtmullaev	1 copy

Naval Research Laboratory
Code/Name
Washington, DC 20375

Code 4700 (Dr. Coffey)	25 copies
4770.1 (I. Vitkovitsky)	1 copy
4776 (D. J. Jenkins)	20 copies
4790 (A. Cooper)	20 copies
4770 Branch Head	10 copies
1001 (Dr. Berman)	1 copy
6000 (Dr. Schindler)	1 copy
4701 (Mr. Brown)	1 copy
4760 (Dr. Robson)	1 copy

On-Site Contractos

Code 4770 (E. Scannell) 1 copy

Department of Energy
Technical Information Center
Oak Ridge, Tennessee 37830

Distribution list for Magnetic Fusion Energy UC-20B, C, D, E and F 346 Copies

N